

# Blast response of walls retrofitted with elastomer coatings

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## Abstract

Recent tests at Tyndall Air Force Research Laboratory show that an elastomer-coated to the inside wall of a building can offer significant protection for occupants by keeping wall fragments together and shielding out blast debris. In this paper, we develop an equivalent single degree-of-freedom model that can be used to predict dynamic response of a polymer-retrofitted concrete brick wall subjected to a stand-off explosion. As an example, we consider the blast response of a 10 ft-square, 8 in-thick concrete brick wall coated with a 0.083 in-thick layer of polyurea. Explosive field tests with a uniformly-distributed pressure pulse of 40 psi peak value and 20 ms pulse duration indicate that the retrofitted wall reaches a maximum deflection of about 7 in. Our analytical model suggested that the blast response of this wall could be simulated assuming the concrete modulus was reduced to about 5% of its original value. The proposed single degree-of-freedom model, which is based on coupling of the bending/membrane resistance of the wall, compared very well with the ABAQUS results when the maximum deflections of the wall were between 1-2 times the wall thickness.

*Keywords: blast response, elastomer-coated concrete wall, equivalent single degree-of-freedom model.*

## 1 Introduction

A major cause of injury and death during bomb attacks on buildings is flying debris due to fragmentation of walls, windows, furniture and equipment. The United States Air Force Research Laboratory at Tyndall Air Force Base recently



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conducted full-scale tests showing that concrete masonry unit (CMU) walls and trailers retrofitted with polyurea or polyurethane coatings will protect occupants when these structures are subjected to a standoff explosion [1]. By bonding tightly to the wall and being able to undergo large membrane deflections without tearing, these elastomers mitigate damaging effects of the blast. The cracked wall is kept together by the elastomer and shields against flying debris from the outside. The ability of the elastomer to stretch extensively without breaking is extremely important because it allows the blast energy to be transferred into viscoelastic strain energy of the wall. This blast energy would have otherwise been used in fragmentation damage – turning pieces of wall, window, furniture and equipment into deadly projectiles.

In this paper, we present an equivalent single degree-of-freedom model that can be used to predict dynamic response of a retrofitted concrete brick wall subjected to a stand-off explosion. The use of equivalent or lumped parameter single degree-of-freedom systems to obtain the blast response of structures is covered extensively in Biggs [2]. This practical approach also forms the basis of blast design codes in the United States [3]. Fragmentation damage of the concrete bricks under blast is a very complex phenomenon. Not only is the initial deformation and fracture properties of concrete sensitive to strain rate and pressure [4], but comminution and post-rubblization flow affect damage evolution in a concrete wall. An average concrete modulus equal to some fraction of the actual modulus of concrete is thus assumed. The reduced concrete modulus is a parameter that can be determined either empirically or with more refined FEA and rate-dependent continuum damage model for concrete [5].

## 2 Problem formulation

Consider a 2ax2a elastomer-coated CMU wall that is clamped at the base and pin-supported at the top. This wall is subjected to a stand-off explosion, which we will describe as a uniformly distributed pressure pulse on the wall:

$$p(t) = p_0 \left( 1 - \frac{t}{\tau} \right) \quad (1)$$

where  $p_0$  is the peak force and  $\tau$  is the decay time of the wall. The retrofitted wall is modeled by the equivalent single degree-of-freedom (SDOF) system shown in Fig. 1, where  $\Delta$  is the deflection at the center of the wall,  $F_{eq} = 4a^2 p(t)$  is the equivalent force of the blast,  $M_{eq}$  is the lumped mass of the CMU and elastomer,  $P_{eq}$  is a function describing the bending and membrane resistance of the wall, and the dots denote derivative with respect to time. The equation of motion for this SDOF system is

$$M_{eq} \ddot{\Delta} + P_{eq}(\Delta) = F_{eq}(t) \quad (2)$$



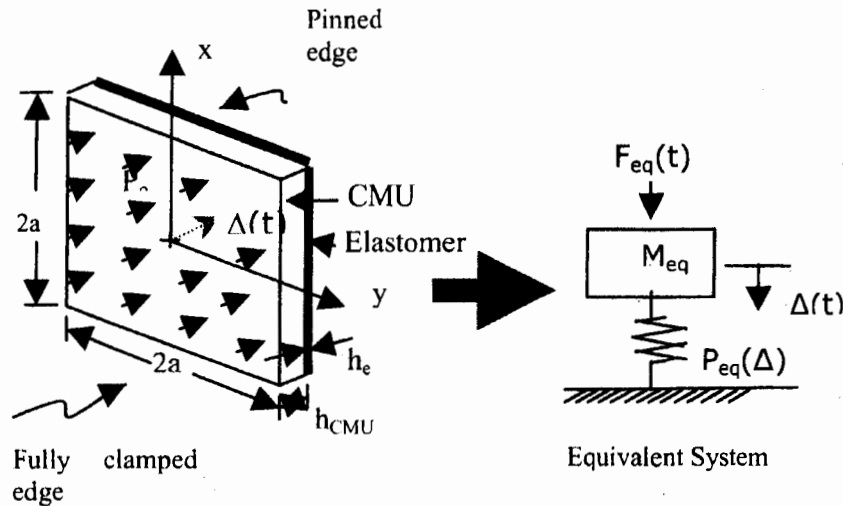


Figure 1: Single degree of freedom model elastomer coated CMU wall subjected to blast.

Lumped or equivalent mass and spring forces are calculated by assuming the following displacement and velocity fields:

$$w(x) = \Delta \left[ 1 - \left( \frac{x}{a} \right)^2 \right] \quad (3)$$

$$\dot{w}(x) = \dot{\Delta} \left[ 1 - \left( \frac{x}{a} \right)^2 \right] \quad (4)$$

where the dot denotes time derivative and the origin of the x-y coordinate system is in the center of the wall.

## 2.1 Equivalent mass

Equating the kinetic energy of an equivalent mass  $M_{eq}$  to the total distributed kinetic energy of wall gives

$$M_{eq} = 4a(\rho_{CMU} h_l + \rho_e h_e) \int_0^a \frac{\dot{w}^2(x)}{\dot{\Delta}^2} dx = \frac{32}{15} a^2 (\rho_{CMU} h_l + \rho_e h_e) \quad (5)$$

where  $\rho_{CMU}$  is the average mass density of the CMU block (including any hollow sections),  $\rho_e$  is the density of the elastomer, and  $h_l$  and  $h_e$  are the thickness of the CMU block and elastomer, respectively.



## 2.2 Bending-membrane resistance function

The bending-membrane resistance function or equivalent spring force  $P_{eq}$  shown in Fig. 1, is found using the principle of minimum potential energy to derive an expression for the static resistance of the wall subjected to a uniformly distributed pressure  $p_0$ . The elastic strain energy of the wall is

$$U = 2a \int_0^a (N_x \epsilon_{x0} + M_x \kappa_x) dx \quad (6)$$

where  $N_x$  and  $M_x$  are the effective membrane force and bending moment of the composite wall, respectively,  $\epsilon_{x0} = \frac{1}{2} \left( \frac{\partial w}{\partial x} \right)^2$  is the midline axial strain for

moderately large deformations and  $\kappa_x = \frac{\partial^2 w}{\partial x^2}$  is the curvature. The effective membrane force and bending moment of the composite wall depend on the modulus of the concrete and elastomer and the geometry of the CMU block.

Consider a standard CMU block with outer dimensions  $h_1 \times h_1 \times b_1$  and two hollow sections of dimensions  $h_2 \times h_2 \times b_2$ , as shown in Fig. 2. The distance to the neutral axis of the composite wall,  $h_{NA}$ , is located by setting the resultant axial forces equal to zero:

$$\int_{-b_1/2}^{b_1/2} \int_{-(h_1+h_e-h_{NA})}^{h_{NA}} (\sigma_{xb} + \sigma_{xm}) dz dy = 0 \quad (7)$$

where the bending  $\sigma_{xb}$  and membrane  $\sigma_{xm}$  stresses are

$$\sigma_{xb} = \begin{cases} \frac{E_c}{(1-\nu_c^2)} z \kappa_x & -(h_1 + h_e - h_{NA}) \leq z \leq h_{NA} - h_e \\ \frac{E_e}{(1-\nu_e^2)} z \kappa_x & h_{NA} - h_e \leq z \leq h_{NA} \end{cases}$$

$$\sigma_{xm} = \begin{cases} \frac{E_c}{(1-\nu_c^2)} \epsilon_{x0} & -(h_1 + h_e - h_{NA}) \leq z \leq h_{NA} - h_e \\ \frac{E_e}{(1-\nu_e^2)} \epsilon_{x0} & h_{NA} - h_e \leq z \leq h_{NA} \end{cases}$$

and  $E_c$  and  $\nu_c$  are the Young's modulus and Poisson's ratio of the cement used to make the CMU block, respectively;  $E_e$  and  $\nu_e$  are the Young's modulus and Poisson's ratio of the elastomer, respectively; and the  $z$ -axis is defined with respect to the neutral axis, as shown in Fig. 2.



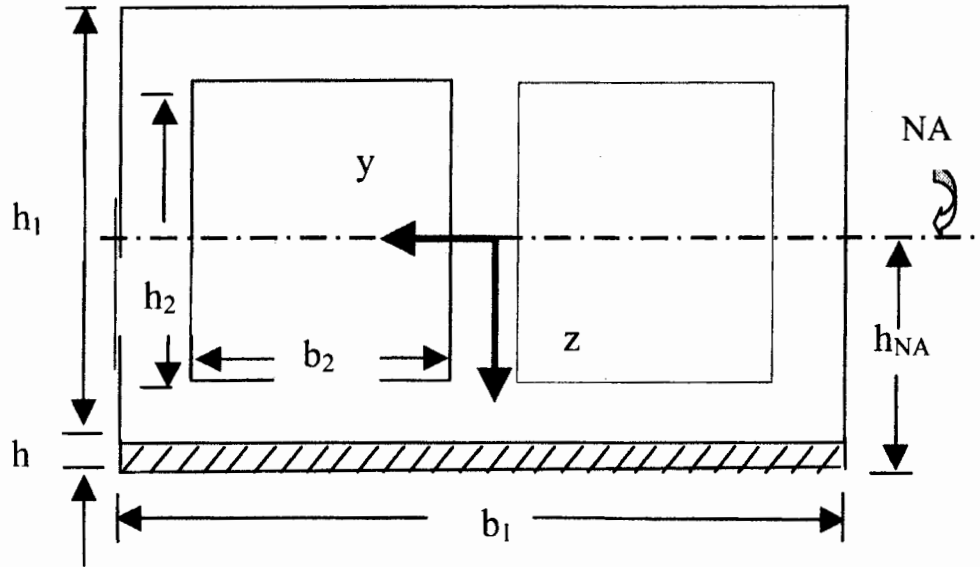


Figure 2: Cross-sectional geometry of CMU block and elastomer.

Substituting  $\sigma_{xb}$  and  $\sigma_{xm}$  into Eq. (7) and integrating gives the following expression for the position of the neutral axis in combined bending and membrane tension:

$$h_{NA} = \frac{E_c(1-\nu_e^2)(h_1^2 + 2h_1h_2 + 2h_e^2) + E_e(1-\nu_c^2)h_e^2}{2h_1E_c(1-\nu_e^2) + 2h_eE_e(1-\nu_c^2)} + \frac{\epsilon_{x0}}{\kappa_x} \quad (8)$$

An effective bending moment for the composite system is found by

$$M_x = \frac{1}{b_1} \int_{-b_1/2}^{b_1/2} \int_{-(h_1+h_e-h_{NA})}^{h_{NA}} \sigma_{xb} z dz dy = D\kappa_x \quad (9)$$

$$\begin{aligned} \text{where } D = & \frac{E_c}{3(1-\nu_c^2)} \left[ (h_{NA} - h_e)^3 - (h_1 + h_e - h_{NA})^3 \right] - \\ & \frac{2b_2E_c}{3b_1(1-\nu_c^2)} \left[ \left( h_{NA} - \left( \frac{h_1 - h_2}{2} \right) - h_e \right)^3 - \left( \left( \frac{h_1 + h_2}{2} \right) + h_e - h_{NA} \right)^3 \right] \\ & + \frac{E_e}{3(1-\nu_e^2)} \left[ h_{NA}^3 - (h_{NA} - h_e)^3 \right] \end{aligned}$$



The effective membrane force is also found by

$$N_x = \frac{1}{b_1} \int_{-b_1/2}^{b_1/2} \int_{-(h_1+h_e-h_{NA})}^{h_{NA}} \sigma_{xm} dz dy = C \varepsilon_{x0} \quad (10)$$

$$\text{where } C = \frac{(b_1 h_1 - 2b_2 h_2)}{b_1} \frac{E_c}{(1-\nu_c^2)} + \frac{h_e E_e}{(1-\nu_e^2)}.$$

Substituting Eqs. (9) and (10) into (6) and using Eq. (3) to integrate the resulting function over  $x$ , gives the following strain energy:

$$U = \frac{8}{a^3} (D_1 \Delta^2 + D_2 \Delta^3 + D_3 \Delta^4) + \frac{8C}{5a^2} \Delta^4 \quad (11)$$

where

$$\begin{aligned} D_1 &= \frac{E_c a}{3(1-\nu_c^2)} \left[ (h_{NA0} - h_e)^3 - (h_1 + h_e - h_{NA0})^3 \right] \\ &\quad - \frac{2b_2 E_c a}{3b_1 (1-\nu_c^2)} \left[ \left( h_{NA0} - \left( \frac{h_1 - h_2}{2} \right) - h_e \right)^3 - \left( \left( \frac{h_1 + h_2}{2} \right) + h_e - h_{NA0} \right)^3 \right] \\ &\quad + \frac{E_e a}{3(1-\nu_e^2)} \left[ 3h_e h_{NA0}^2 - 3h_e^2 h_{NA0} + h_e^3 \right] \\ D_2 &= \frac{E_c a}{3(1-\nu_c^2)} \left[ (h_{NA0} - h_e)^2 - (h_1 + h_e - h_{NA0})^2 \right] \\ &\quad - \frac{2b_2 E_c a}{3b_1 (1-\nu_c^2)} \left[ \left( h_{NA0} - \left( \frac{h_1 - h_2}{2} \right) - h_e \right)^2 - \left( \left( \frac{h_1 + h_2}{2} \right) + h_e - h_{NA0} \right)^2 \right] \\ &\quad + \frac{E_e a}{3(1-\nu_e^2)} \left[ 2h_{NA0} - h_e \right] \\ D_3 &= \frac{E_c a}{5(1-\nu_c^2)} \frac{(h_1 b_1 - 2h_2 b_2)}{b_1} + \frac{E_e a h_e}{5(1-\nu_e^2)}. \end{aligned}$$

The potential of the work done by the external loads is

$$V = - \iint_S p_o w dx dy = \frac{-8a^2 p_o}{3} \Delta \quad (12)$$



The total potential energy is

$$\Pi = U + V = \frac{8}{a^3} (D_1 \Delta^2 + D_2 \Delta^3 + D_3 \Delta^4) + \frac{8C}{5a^2} \Delta^4 - \frac{8a^2 p_o}{3} \Delta \quad (13)$$

Minimizing the total potential energy with respect to  $\Delta$  gives the following pressure equilibrium equation:

$$p_o = \frac{3}{5a^5} [10D_1 \Delta + 15D_2 \Delta^2 + 4(aC + 5D_3) \Delta^3] \quad (14)$$

Thus the equivalent force equilibrium equation is

$$P_{eq} = p_o (2a)^2 = \frac{12}{5a^3} [10D_1 \Delta + 15D_2 \Delta^2 + 4(aC + 5D_3) \Delta^3] \quad (15)$$

### 3 An example

As an example, consider the blast response of a 10 ft-square wall constructed from 8x8x16 standard CMU blocks [6] and coated on the distal side of the explosion with a 0.083 in-thick layer of polyurea. Explosive field tests with a blast pressure of peak value  $p_o = 40$  psi and pulse duration of  $\tau = 20$  ms indicate that this wall will reach a maximum deflection of about 7 in.

Geometric parameters of the composite wall are as follows:

$a = 60$ in	half-width and half-height of wall
$h_1 = 7.625$ in	outer block height
$h_2 = 5.125$ in	height of block hollow section
$b_1 = 15.625$ in	outer block base
$b_2 = 6.3125$ in	base of block hollow section
$h_e = 0.083$ in	elastomer thickness

Mechanical properties of the concrete are as follows:

$E_c = 3,000$ ksi	Young's modulus of concrete
$\nu_c = 0.15$	Poisson's ratio of concrete
$\rho_{CMU} = 7.2 \times 10^{-5}$ lbs <sup>2</sup> /in <sup>4</sup>	mass density of homogenized CMU block





Mechanical properties of the polyurea are taken from experiments [7]:

$$\sigma = \begin{cases} E_{e1}\epsilon & 0 \leq \epsilon \leq \epsilon_y \\ (E_{e1} - E_{e2})\epsilon_y + E_{e2}\epsilon & \epsilon \geq \epsilon_y \end{cases},$$

$$\text{where } E_{e1} = \begin{cases} 17.5 + 27.86\dot{\epsilon} & 0 \leq \dot{\epsilon} \leq 7 \\ 212.5 & \dot{\epsilon} \geq 7 \end{cases}, E_{e2} = \begin{cases} 3 + 4.05\dot{\epsilon} & 0 \leq \dot{\epsilon} \leq 1.7 \\ 10 & \dot{\epsilon} \geq 1.7 \end{cases},$$

$$\epsilon_y = \begin{cases} 0.16 - 0.042\dot{\epsilon} & 0 \leq \dot{\epsilon} \leq 3.6 \\ 0.01 & \dot{\epsilon} \geq 3.6 \end{cases}, \text{ and } E_{e1} \text{ and } E_{e2} \text{ are in ksi and } \dot{\epsilon} \text{ is in s}^{-1}.$$

$$\nu_e = 0.5 \quad \text{Poisson's ratio of elastomer}$$

$$\rho_e = 9.076 \times 10^{-5} \text{ lbs}^2/\text{in}^4 \quad \text{mass density of polyurea}$$

Substituting the equivalent mass, equivalent spring force, and equivalent loads into Eq. (2) gives a nonlinear, nonhomogeneous second-order differential equation, where  $C, D_1, D_2$  and  $D_3$  are rate-dependent functions. This differential equation is solved with initial conditions  $\Delta = 0$  and  $\dot{\Delta} = 0$  and reduced concrete modulus in the range of 7.25-145 ksi, using MATLAB. These reduced concrete moduli were chosen so that the maximum deflection of the wall would be roughly between 1-2 times the thickness of the wall since this is the range in which the bending-membrane tension would occur in the wall. At  $E_c = 145$  ksi, the maximum deflection of the wall is about 7 in, which is the maximum deflection that was obtained from the blast test. Since the original modulus of concrete is  $E_c = 3,000$  ksi, cracking of the concrete in the CMU block must have caused the modulus to decrease by about 4.8% of its original value during the blast. A parametric study on the influence of the elastomer modulus indicates that wall deflections are fairly insensitive to elastic properties of the polyurea. Thus the only important role of the elastomer is to keep concrete fragments together so that the blast energy can be absorbed in bending and membrane stretching of a "weakened" concrete wall. Without the elastomer, CMU blocks would have separated into flying debris and the wall would have no resistance to the blast.

Since the response of the wall is not strongly dependent on mechanical properties of the elastomer, we assume the concrete and the elastomer as two rate-independent, linear elastic materials and use ABAQUS Explicit to find the central deflection of the elastomer-coated wall in our example problem. The modulus of the elastomer is chosen as  $E_e = 32.5$  ksi and all other material parameters except for the reduced concrete modulus are kept the same as in the example. Variations of the maximum deflection with the reduced modulus of concrete from ABAQUS Explicit and the SDOF model are compared in Fig. 3.



The analytical model over-predicts FEA results by about 58% when the maximum deflection from ABAQUS is 4.5 in, but the over-prediction decreases to about 3.2% when the deflection is 16.5 in. In general, FEA and analytical predictions approach each other as the deflection increases from about 8 in, which is the thickness of the wall. This is to be expected since the SDOF was based on the bending-membrane resistance of the wall and this theory breaks down if deflections are too small (less than the thickness of wall) or too large (many times the wall thickness).

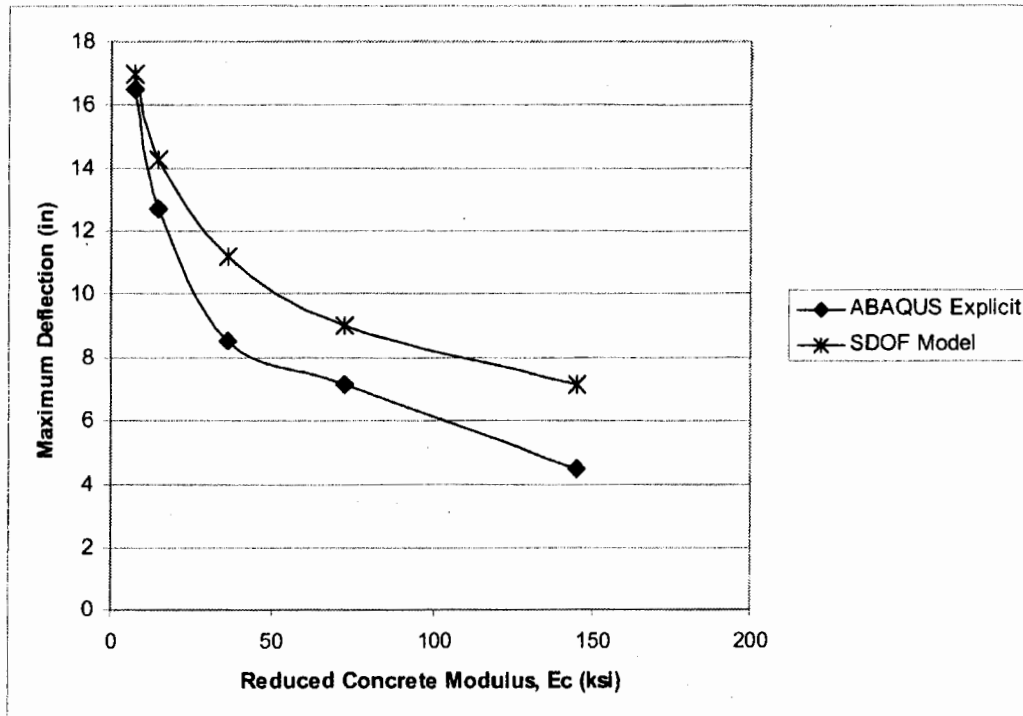


Figure 3: Comparison of predicted maximum deflection from equivalent single degree-of-freedom (SDOF) model and ABAQUS Explicit ( $E_e = 32.5$  ksi).

## 4 Conclusions

An equivalent single degree-of-freedom model of an elastomer-coated wall subjected to blast was presented in this paper. Equivalent mass, force resistance and loads were derived by assuming parabolic shape functions for the transverse deflection and velocity of the wall. As an example, the problem of a blast on a 10 ft-square wall constructed from 8x8x16 standard CMU blocks and coated on the distal side of the explosion with a 0.083 in-thick layer of polyurea, was considered. Explosive field tests with a blast pressure of peak value 40 psi and pulse duration of 20 ms indicated that this wall reaches a maximum deflection of about 7 in. It was found that the concrete modulus must have been reduced to 145 ksi or about 5% of its original value during the blast.



The analytical model over-predicted FEA results by about 58% when the maximum wall deflection was about half of the thickness, but it was only about 3.2% higher than the ABAQUS solution when the deflection was about twice the wall thickness. Since the proposed single degree-of-freedom model was based on coupling of the bending and membrane resistance of the wall, it should only be used when the maximum deflection of the wall is expected to be greater than the wall thickness.

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